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FORT EUSTIS, VIRGINIA

STRUCTURAL ANALYSIS OF THREE CRASH IMPACT CONFIGURATIONS IN STEEL TUBE AND FABRIC AIRCRAFT

A Preliminary Report

May 1964

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prepared by :

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This report was prepared by Aviation Crash Injury Research (AVSER), a division of the Flight Safety Foundation, Inc., under the terms of Contract DA 44-177-AMC-888(T). Views expressed in the report have not been reviewed or approved by the Department of the Army; however, conclusions and recommendations contained herein are concurred in by this Command.

Until recently, very little was known about the kinematics of an aircraft crash. A series of dynamic crash tests conducted by AVSER over the past two years, together with a thorough review of the literature relative to this subject, has provided data upon which to base recommendations for changes to existing military specifications as they relate to crashworthiness aspects of the aircraft structures.

This report covers an approach to the development of mathematical techniques for predicting the structural response of airframes under crash conditions. A Piper 116 J-3 model was developed for three crash conditions, and sample computations were conducted to determine critical impact velocity and peak acceleration. The mathematical models outlined are not the optimum; however, they demonstrate the feasibility of developing rigorous computation prediction techniques which could be applied to various aircraft structures.

Development of mathematical airframe impact/structural-strength prediction techniques is required for use in the evaluation and design of future aircraft.

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PRELIMINARY REPORT

Technical Report
AvSER 63-9

Prepared by
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for
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SYMBOLS

A_i	area of i^{th} tube
d_i	mean diameter of i^{th} tube
F_{cr_i}	critical buckling load for i^{th} tube
F_w	maximum wing tip force
F_{w1}	that portion of F_w required to rotate wing quasi-statically, i.e., required to collapse wing connection structure
F_{w2}	that portion of F_w required to impart angular momentum to wing mass
I_i	moment of inertia of i^{th} tube
I_w	mass moment-of-inertia of wings
K	elastic modulus of receiving medium (force/unit length compression)
L_i	length of i^{th} tube
m	total mass of aircraft
m'	mass of aircraft excluding engine
m_m	mass of occupant
m_w	mass of wings
P_1	longitudinal force required to collapse first bay
r	distance from center of rotation to the point of application of F_{w1}
s	length of first bay
$\overline{s}$	total displacement of center of mass of aircraft less engine during impact ($\overline{s}$ = x + s)
t	duration of impact

t_i	tube wall thickness, i^{th} tube
U_g	energy required to collapse (completely) the main gear
v	initial velocity of aircraft
v_1	velocity of fuselage upon its contact with ground
v_o	initial vertical velocity of aircraft
x	maximum deflection of receiving medium
x_i, y_i, z_i	coordinates of forward end of i^{th} tube
$\overline{x}_i$, $\overline{y}_i$, $\overline{z}_i$	coordinates of aft end of i^{th} tube
Y	a dimensionless variable
Z	a dimensionless variable
δ_i	reduction in length of the i^{th} tube (associated with the assumed mechanism)
Δ	total deflection of seat-cushion combination associated with a critical vertical deceleration of occupant
$\delta\theta$	angle of wing rotation
λ_{xi}	direction cosine of i^{th} tube for the longitudinal direction
σ_y	yield stress
ω	maximum angular velocity of wings

SUMMARY

This report covers the development of limited mathematical models to predict in qualitative terms (see Limitations of Analysis, page 24) the crashworthiness of a light airframe structure (of the Piper J-3 type) under three impact conditions.

Two indices of crashworthiness were selected: (1) the aircraft velocity required to cause structural collapse short of general cabin collapse, and (2) the maximum deceleration applied to the occupants during the crash.

Engineering analysis and physical principles are applied to each appropriate mathematical model to develop the crashworthiness indices. Sample calculations were made for the Piper J-3 in three typical crash impact configurations: nose impact, nose and wing impact, and impact on the landing gear (considering only the vertical velocity component).

The preliminary study reveals that the crashworthiness indices selected are potentially useful in evaluating structural modifications such as the eight items discussed.

CONCLUSIONS

1. This preliminary study indicates that the crashworthiness indices selected are potentially useful in evaluating structural modifications to increase the crashworthiness of a given aircraft structure.

2. The efficiency of energy absorption of the first bay has a significant effect upon the selected indices and hence upon crashworthiness.

3. Considerable promise of improved crashworthiness appears probable from changes in cabin structure. If the cabin were made much stronger than other airframe structure, impact forces would be transmitted through the cabin and the energy absorbed in deformation elsewhere.

4. This preliminary study indicates that a more extensive investigation of the collapse mechanism involving computer simulation and time sequence is feasible.

RECOMMENDATIONS

It is recommended that:

1. Suitable experiments be designed and conducted to check the validity of the theoretical assumptions, thus removing some of the current information gaps.

2. For a more general treatment, three approaches be explored in order to determine which best serves the study objectives; upon selection of the best approach, it be adopted for a general computer program. The three approaches are described as follows:

   a. Separate analyses would be developed for aircraft of basically different structures with separate subroutines for different substructures. If a final computer program designed to analyze any light aircraft were to evolve from this approach, logical branching to alternate routines and subroutines would be required.

   b. A second approach to generality would entail a basic description of any airframe element (a tube, a monocoque bay, etc.) in terms of fundamental properties (geometric and structural behavior properties). Then, by locating these elements with respect to one another, any airframe could be mathematically simulated. A simulated impact configuration would be applied to such a structure on the computer. Then, employing the physical principles involved, the collapse mode and force transmission would be determined.

   c. A third approach, of possible usefulness, would involve an abstraction of structural behavior from the external impact conditions. Based upon statistical accident data and engineering analysis, a number of potential collapse modes or mechanisms would be selected for examination. For each of these mechanisms, crashworthiness indices would be developed, such as specific energy absorption or a specific critical force index. A meaningful computer program that followed this approach would compute the crashworthiness indices for a given structure and would also provide statistical norms and "significance" weight factors for each of the indices computed. The index norm and weight factor would conceivably depend upon the design mission and performance features of the particular subclass of aircraft.

STATEMENT OF PURPOSE

The objective of the current study is to determine significant indices of crashworthiness as related to the structure of light aircraft. The question for which an answer is sought is simply: How may proposed changes in a given structure, intended to increase crashworthiness, be evaluated most meaningfully? It is not intended that these indices be used to rate crashworthiness of various makes and models of aircraft relative to one another (as other factors would enter into such a rating) but rather that these indices should serve to point directions of potential improvement in a given structural design. Perhaps useful generalizations, such as a recognition of the critical nature of energy-absorbing properties of the first bay, will emerge as the study progresses.

PHILOSOPHY OF ANALYSIS

Survivability or nonsurvivability of a lightplane accident is generally determined by the degree to which the aircraft structure around the occupants collapses or retains its shape. In an accident in which the cabin structure collapses to a fraction of its normal volume, the chances of occupant survival are correspondingly reduced. Additionally, if decelerations are great enough to exceed the limits of the occupant restraint systems, severe injury could also result.

The preliminary study employs as crashworthiness indices: (1) the maximum impact velocities which can be sustained by the aircraft in various impact configurations without causing general cabin collapse, or in the case of vertical motion, without causing spinal injury; and (2) the decelerations associated with these limiting velocities.

Nearly all light aircraft in use today are of two types of construction: (1) welded steel tubing with fabric covering, or (2) all-metal semi-monocoque. The first portion of this study deals with tube and fabric structure, using the Piper J-3 as an example.

As it is not feasible to consider all possible accident impact configurations, a representative few are selected for analysis that typify conditions most frequently found in actual accidents. A survey of a representative group of survivable Piper J-3 accidents gives the following distribution of impact configurations classified on the basis of the contact points during the primary impact.

IMPACT CONFIGURATIONS OF 116 PIPER J-3 ACCIDENTS

Impact Configuration	No. of Cases	Percent of Cases
Nose only	33	28.45
Nose and wing	31	26.72
Nose and main landing gear	22	18.97
Nose, landing gear and wing	13	11.21
Landing gear only	8	6.89
Landing gear and wing	5	4.31
Wing only	4	3.45
	116	100.00

Using the foregoing table as a guide, mathematical models of impact configurations are employed with the same primary contact points. It is assumed that the flight path is along the longitudinal axis of the aircraft, except the "gear only" configuration. An ideal elastic receiving medium, simulating a typical soil, is assumed for purposes of energy absorption calculations.

ANALYTICAL METHOD

To estimate the critical velocity for a given impact configuration, that is, the velocity of the aircraft required to cause structural collapse short of general cabin collapse, an energy approach is employed. The kinetic energy of the aircraft just prior to impact is equated to the energy dissipated in various forms during the impact. This latter includes the energy to compress the receiving medium, the work of plastic collapse of aircraft structure, and the energy dissipated in internal damping throughout the aircraft (other than that of visible plastic deformation).

To determine the maximum deceleration experienced by the occupants, the second index of crashworthiness, a force transmission analysis is considered in which the forces required to cause each stage of the progressive collapse are computed. The maximum force found may be used to obtain the maximum deceleration experienced if the effective mass of the aircraft is known.

If the mechanism or mode of collapse is not evident, it may be useful to bracket the unknown collapse force by upper-bound and lower-bound values. In particular, for an assumed mechanism, the load required to sustain the mechanism will be greater than or equal to the actual collapse forces; hence, the load will form an upper bound. Correspondingly, for an assumed stable internal force distribution satisfying equilibrium, the associated loads are always less than or equal to the required collapse forces, constituting a lower bound.

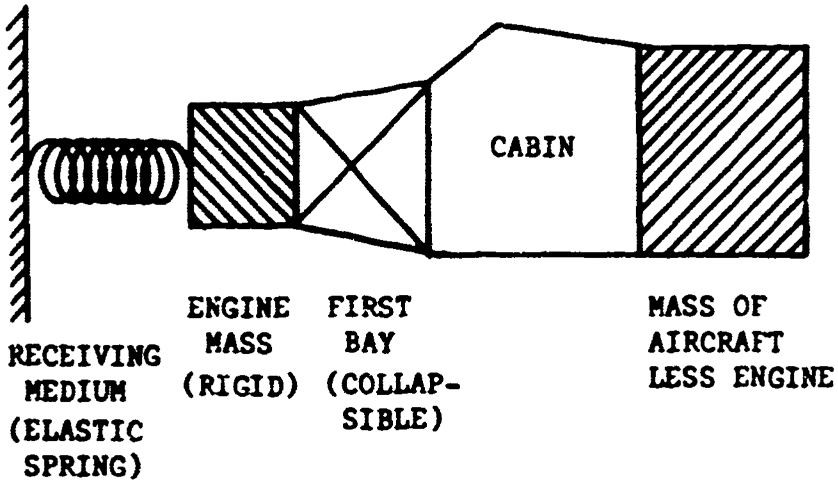
As a practical computational technique, a probable collapse mode is first assumed which would yield an upper bound. This then would be examined from a force distribution viewpoint to determine whether it also satisfies the lower-bound requirements. If not, a lower bound can be obtained simply by a proportionate reduction in force magnitudes to the point where the lower-bound requirements are satisfied. If the resulting upper and lower bounds are sufficiently close to each other, their average constitutes a satisfactory approximation to actual collapse forces.

APPLICATION OF METHOD

Three impact configurations have been considered initially for which crashworthiness indices are developed in terms of structural parameters (tube and fabric structure only, to date). These three configurations are: (1) nose-only contact, (2) nose-and-wing contact, and (3) gear-only contact, considering vertical velocity. The detailed analysis of each of these is treated separately as follows.

NOSE-ONLY IMPACT CONFIGURATION

A simplified mathematical model using lumped parameters is presented schematically in the following diagram:



The pattern of impact and collapse is assumed to entail a compression of the receiving medium initially, with the reaction force increasing linearly to the point where the critical axial load causes a complete crushing of the first bay (provided the forces required are less than the critical forces for cabin collapse).

Symbol Notation:

K	elastic modulus of receiving medium (force/unit length compression)
v	initial velocity of aircraft
x	maximum deflection of receiving medium

P_1 longitudinal force required to collapse first bay

s length of first bay

m' mass of aircraft excluding engine

m total mass of aircraft

Considering the entire aircraft in an energy balance, the kinetic energy prior to impact would equal the energy required to compress the receiving medium, the energy required to crush the first bay, and the energy dissipated in internal damping throughout the aircraft. The latter quantity is of a nebulous nature, although dependent in part upon the magnitude of P_1 and the length s. As an operating expedient, the average axial force transmitted through the first bay is taken as half of P (the critical first-bay load under static conditions), although it is known that a column force falls off markedly after buckling has started. This simplifying assumption of treating the average force as half the maximum static force becomes plausible when one considers the nebulous nature of internal damping energy, the existence of obstructions to the free column buckling of the tubular elements, the direct axial resistance offered by nonstructural elements (sheet-metal covering, fuel tank, etc.), and the dynamic column load over-shoot associated with high deformation rates. Thus the energy balance may be written:

$$\underbrace{\frac{mv^2}{2}}_{\text{initial kinetic energy}} = \underbrace{\frac{Kx^2}{2}}_{\text{receiving medium energy}} + \underbrace{\frac{P_1 s}{2}}_{\text{aircraft deformation energy}} \qquad (1)$$

In a similar fashion, an energy balance may be written for the aircraft without engine, in terms of motion of its mass center. The principle used may be stated as follows: The work of external forces displaced through the mass center displacement equals the change in kinetic energy associated with the velocity of the mass center. Thus, for an average external force $\frac{P_1}{2}$, and a total displacement of mass center equal to $(x + s)$,

$$-\frac{P_1}{2}(x+s) = -\frac{m'v^2}{2} \qquad (2)$$

negative work of $\frac{P_1}{2}$ change in kinetic energy

Solving equation (2) for x and substituting into (1) yields

$$mv^2 = K\left(\frac{m'v^2}{P_1} - s\right)^2 + P_1 s \ .$$

This may be simplified to

$$Z^2 - \left(2 + \frac{P_1}{Ks}\frac{m}{m'}\right)Z + \left(1 + \frac{P_1}{Ks}\right) = 0 \qquad (3)$$

where Z is a symbol for the dimensionless quantity

$$Z = \frac{m'v^2}{P_1 s} \ . \qquad (4)$$

If P_1 is first determined, then by means of equations (3) and (4), the critical velocity v may be computed.

For the computation of P_1, the longitudinal force required to collapse the first bay, the following notation is introduced:

x_i, y_i, z_i	coordinates of forward end of i^{th} tube
$\bar{x}_i$, $\bar{y}_i$, $\bar{z}_i$	coordinates of aft end of i^{th} tube
t_i	tube wall thickness, i^{th} tube
d_i	mean diameter of i^{th} tube
L_i, A_i, I_i	length, area, and moment of inertia of i^{th} tube
F_{cr_i}	critical buckling load for i^{th} tube
λ_{xi}	direction cosine of i^{th} tube for the longitudinal direction
σ_y	yield stress

For a sufficiently slender pinned-end column, the critical load is given by

$$F_{cr} = \frac{\pi^2 EI}{L^2} ;$$

and for a fully fixed-end column,

$$F_{cr} = \frac{4\pi^2 EI}{L^2} .$$

In a welded tubular structure, some intermediate end fixity condition exists; hence,

$$F_{cr_i} = \frac{2\pi^2 EI_i}{L_i{}^2} \tag{5}$$

is taken as a plausible value for the buckling load of a slender column.

A column of low slenderness ratio, on the other hand, would undergo plastic rather than elastic buckling, the critical load for which may be crudely approximated by yield stress times the cross-sectional area, or

$$F_{cr_i} \doteq \sigma_y A_i \ . \tag{6}$$

For circular steel tubes,

$$E = 30 \times 10^3 \text{ ksi}$$

$$I \doteq \frac{\pi d^3}{8}(t)$$

$$A \doteq \pi(d)t \ .$$

Equations (5) and (6) thus reduce to

$$F_{cr_i} = 23.2 \left(\frac{10d_i}{L_i}\right)^2 100t_i d_i \tag{7}$$

$$F_{cr_i} = \sigma_y \pi t_i d_i \tag{8}$$

where the smaller value of F would govern.

From geometry

$$L_i = \sqrt{(\bar{x}_i - x_i)^2 + (\bar{y}_i - y_i)^2 + (\bar{z}_i - z_i)^2} \ . \tag{9}$$

The longitudinal component of F_{cr_i} is obtained by multiplying by direction cosine, λ_{xi}, where λ_{xi} is given by

$$\lambda_{xi} = \frac{\bar{x}_i - x_i}{L_i} \ .$$

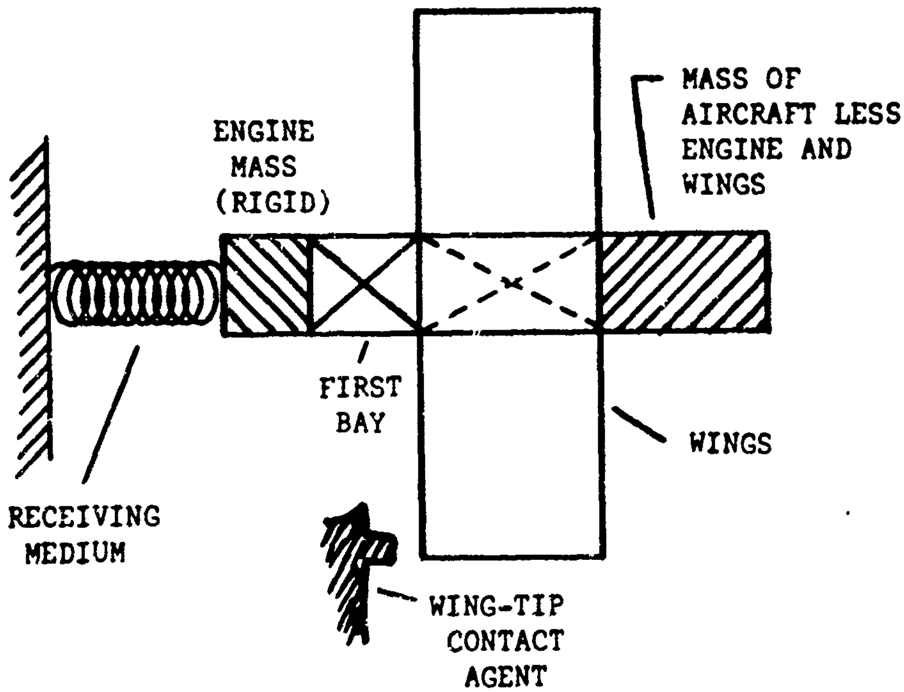
Consequently, the total critical longitudinal load, P_1, is

$$P_1 = \sum_i F_i \lambda_{xi} \ . \tag{10}$$

An example of computations for the nose-only type of impact is given in the Appendix, pages 27, 28, and 29.

NOSE-AND-WING CONTACT CONFIGURATION

A schematic mathematical model of the aircraft for this impact configuration is presented below.



The symbol notation employed for the analysis of this configuration appears below (previously introduced notation would still apply).

F_w	maximum wing-tip force
F_{w1}	that portion of F_w required to rotate wing quasi-statically, i.e., required to collapse wing connection structure
F_{w2}	that portion of F_w required to impart angular momentum to wing mass
m_w	mass of wings
I_w	mass moment-of-inertia of wings
r	distance from center of fuselage to wing tip (point of force application)
ω	maximum angular velocity of wings
$\bar{s}$	total displacement of center of mass of aircraft less engine during impact ($\bar{s} = x + s$)
t	duration of impact
Y	a dimensionless variable

Simplifying assumptions made in the ensuing analysis are as follows:

a. Under the crash kinematics, the wing acquires an angular momentum where the maximum angular velocity, ω, is approximated by

$$\omega = \frac{v}{r},$$

v still denoting the velocity of the aircraft prior to impact.

b. The average wing force during the impact time interval is assumed to be half of the maximum force, F_w.

c. The average velocit, ⌐ ᴗne aircraft center of gravity (excluding engine) durıng the impact is assumed to be half of the initial velocity, v.

d. The fuselage does not rotate due to eccentric loading of the wing, but continues its free-path movement linearly.

e. The wing contact agent is fully rigid and therefore does not deflect.

Considering an energy balance associated with motion of the center of mass of the aircraft less engine, we have

$$\left(\frac{P_1 + F_w}{2}\right)\bar{s} = \frac{1}{2} m' v^2 .$$

Then, since $F_w = F_{w1} + F_{w2}$,

$$\frac{(P_1 + F_{w1})\bar{s}}{2} + \frac{F_{w2}}{2}\bar{s} = \frac{1}{2} m' v^2 .$$

However, $\frac{F_{w2}}{2}\bar{s} \doteq$ work relative to the center of mass of the wing of force F_{w2} upon the wing;

hence,

$$\frac{F_{w2}}{2}\bar{s} = \frac{1}{2} I_w \omega^2 .$$

Assuming a uniform-geometry wing (c. g. at geometrical center, constant chord, thickness and mass distribution), the inertia may be expressed as that of a thin straight rod:

$$I_w \doteq \frac{m_w r^2}{3} .$$

Consequently,

$$\frac{P_1 + F_{w1}}{2}\bar{s} = \frac{1}{2} m' v^2 - \frac{1}{2}\frac{(m_w r^2)}{3}\frac{v^2}{r^2} ,$$

or

$$\bar{s} = \frac{m' v^2}{P_1 + F_{w1}}\left[1 - \frac{m_w}{3m'}\right] . \tag{11}$$

From kinematics, the time may be found:

$$\bar{s} = \frac{v}{2} t$$

$$t = \frac{2m'v}{P_1 + F_{v1}}\left(1 - \frac{m_v}{3m'}\right) . \qquad (12)$$

The angular impulse/angular momentum relationship may be used to determine the force F_{w2}:

Angular Impulse $= rF\Delta t = rFt$

$$t = \Delta t = (t_f - t_o)$$

$t_o =$ time initial

$t_f =$ time final

Angular Momentum $= I\omega$.

Since the angular impulse of the external forces acting on a system of particles with respect to an axis fixed in space during any time interval is equal to the change of angular momentum of the system with respect to the same axis, the following expression may be written:

$$Frt = I(\omega_f - \omega_i)$$

$$\omega_f = \omega$$
$$\omega_i = 0$$

$$Frt = I\omega .$$

$$\frac{F_{w2}}{2} tr = I_w \omega = \frac{m_w r^2}{3} \frac{v}{r}$$

$$F_{w2} = \frac{2m_w v}{3t}$$

Thus, substituting equation (12) and simplifying,

$$F_{w2} = \frac{P_1 + F_{w1}}{\left(3\,\frac{m'}{m_w} - 1\right)} \, . \tag{13}$$

An energy balance for the entire aircraft may be employed to determine the critical initial velocity, v:

$$\underbrace{\frac{mv^2}{2}}_{\substack{\text{initial}\\ \text{kinetic}\\ \text{energy}}} = \underbrace{\frac{Kx^2}{2}}_{\substack{\text{work on}\\ \text{receiving}\\ \text{medium}}} + \underbrace{\frac{P_1 s}{2}}_{\substack{\text{energy of}\\ \text{deformation}\\ \text{in first bay}}} + \underbrace{\frac{F_{w1}\bar{s}}{2}}_{\substack{\text{energy of}\\ \text{deformation}\\ \text{for wing rotation}}} + \underbrace{\frac{I_w \omega^2}{2}}_{\substack{\text{kinetic}\\ \text{energy}\\ \text{of wing}}} \tag{14}$$

Substituting for x, I_w, and ω the expressions

$$x = \bar{s} - s \, ,$$

$$I_w = \frac{m_w r^2}{3} \, ,$$

$$\omega = \frac{v}{r} \, ,$$

equation (14) becomes

$$mv^2 \left(1 - \frac{m_w}{3m}\right) = K\left(\bar{s}^2 - 2\bar{s}s + s^2\right) + P_1 s + F_{w1}\bar{s} \, .$$

Noting that $\left[1 - \frac{m_w}{3m'}\right] \doteq \left[1 - \frac{m_w}{3m}\right]$, equation (11) may be introduced into the left-hand side of the above:

$$\frac{m}{m'}\left(P_1 + F_{w1}\right)\bar{s} = K\left(\bar{s}^2 - 2\bar{s}s + s^2\right) + P_1 s + F_{w1}\bar{s}$$

or

$$Y^2 - \left[2 + \frac{m}{m'}\,\frac{P_1 + F_{w1}}{Ks} - \frac{F_{w1}}{Ks}\right] Y + \left[1 + \frac{P_1}{Ks}\right] = 0 \tag{15}$$

where

$$Y = \frac{\bar{s}}{s} \text{ or } \frac{m'v^2}{(P_1 + F_{w1})s}\left[1 - \frac{m_v}{3m'}\right]. \tag{16}$$

Equation (15) can be solved for the dimensionless quantity Y (assuming P_1 and F_{w1} are known quantities at this point). The critical velocity v can be obtained then from expression (16) for Y.

Turning attention next to the computation of F_{w1} (the P_1 computation having previously been indicated, expression 10), a plausible mechanism of airframe collapse at the wing attachment may be considered. One such mechanism is a rigid-body rotation (relative to the fuselage) of the wings and top of the cabin (between the wings) about the geometric center of the attachment points. Under such a rotation, deformation occurs in the fuselage bays adjacent to the wing attachments, the pattern of which is determined from geometry and from the assumption of no extension of any member. The energy of deformation is then computed as

$$\sum F_{cr_i}\delta_i = \text{energy of deformation}$$

where

F_{cr_i} is the critical column load for the i^{th} tube

δ_i is the reduction in length of the i^{th} tube (associated with the assumed mechanism)

Equating the external work done by the applied force F_{w1} to the energy of deformation, we obtain

$$F_{w1} r\delta\theta = \sum F_{cr_i}\delta_i$$

or

$$F_{w1} = \frac{\sum F_{cr_i}\delta_i}{r\delta\theta} \tag{17}$$

where

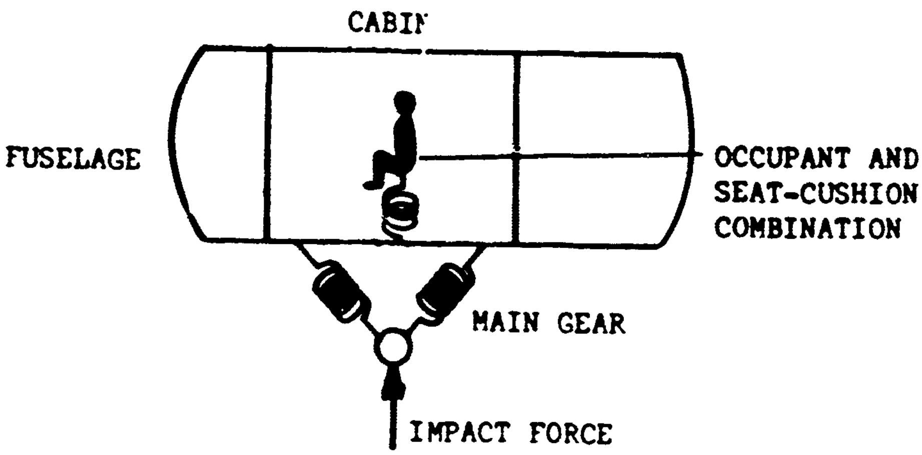
r is the distance from the center of rotation to the point of application of F_{w1}, and

$\delta\theta$ is the angle of wing rotation.

Expression (17) represents an upper bound on the actual value of F_{wi} (employing the upper-bound principle discussed on page 3). To obtain a lower bound, consideration is given to the equilibrium of the joints in the airframe truss. In satisfying equilibrium at each joint, a reduction in force magnitudes may be required so as not to exceed critical loads in either tension or compression in any one member. Moving from joint to joint, a new set of F_i is found with values that are less than or equal to the critical values and that also satisfy joint equilibrium. With these values, again using expression (17), a lower bound on F_{wi} is found. Since the lower- and upper-bound values of F_{wi} are not likely to differ significantly, their average is then taken as a reasonable value. See the Appendix, pages 30, 31, and 32, for sample computations on the nose-and-wing impact configuration.

GEAR-ONLY CONTACT, WITH VERTICAL VELOCITY ONLY CONSIDERED

A schematic diagram for this impact configuration is shown below.



The main gear is treated as a collapsible spring (in general, nonlinear). The airframe is considered to remain essentially intact for this type of impact, as serious spinal injury could result from a vertical deceleration considerably less than that required to collapse the cabin. A standard seat-cushion combination is assumed, which is simulated by a linear spring with a spring constant k_s (force per deflection).

Considering the occupant-seat system, the following energy balance

may be written for the impact of the fuselage with the ground:

Loss in kinetic energy of man = increase in elastic energy of seat and cushion,

or

$$\frac{m_m v_1^2}{2} = \frac{k_s \Delta^2}{2}$$

where

m_m mass of occupant

v_1 velocity of fuselage upon its contact with the ground

Δ total deflection of seat-cushion combination associated with a critical vertical deceleration of the occupant.

Therefore,

$$v_1 = \sqrt{\frac{k_s}{m_m}}\,\Delta \ . \tag{18}$$

To obtain the original vertical velocity of the aircraft prior to contact of the gear with the ground, an energy balance associated with the gear collapse phase may be written as follows:

$$\frac{m v_o^2}{2} = U_g + \frac{m v_1^2}{2}$$

where

v_o is initial vertical velocity of aircraft

U_g is energy required to collapse (completely) the main gear.

Hence,

$$v_o = \sqrt{\frac{2U_g}{m} + v_1^2} \ . \tag{19}$$

Thus, assuming that the force-displacement relation for the main gear is known, U_g may be determined (the area under the force-displacement curve). Then, substituting for v_1 from equation (18), the critical vertical velocity v_0 for this impact configuration is calculated by equation (19). See the Appendix, pages 33, 34, and 35 for sample computations on the gear-only type of impact configuration.

DISCUSSION

The objective of the present analysis, as stated earlier, is to provide a basis for evaluating proposed structural modifications from the crashworthiness viewpoint or to indicate design possibilities that would improve crashworthiness. Changes in structure might take the form of any of the following:

1. General strengthening of a given bay to increase its collapse load.
2. Increase in energy-absorption capacity of a given bay without increasing the collapse load.
3. General geometric changes, such as lengthening a bay or relocating the cabin.
4. Change in component design, such as a change in the type of the main landing gear.
5. Seat or seat tie-down changes.
6. Minor structural changes, such as strengthening a single tube or adding a brace.
7. Changes designed to reduce effective mass of the aircraft upon impact.
8. Cabin modifications to increase cabin strength or perhaps to produce a more favorable mode of collapse.

Each of these is considered separately below, in the light of the analysis and computations made so far (reference the Appendix).

1. General Strengthening of a Given Bay

For a nose-only-contact impact configuration, an increase in the first-bay collapse load would increase the critical impact velocity, thus reducing the probability of cabin collapse during an accident. For example, if P_1 (for the Piper J-3 investigated) were increased by 50 percent, the critical velocity would increase from 34 miles per hour to 45 miles per hour provided the first-bay increase in strength were accomplished without overloading the individual structural members of the cabin. However, such a change would also increase the decel-

eration experienced by the cabin floor from 30.2 G to approximately 45 G. Hence, depending upon the relative significance of the two effects produced (one favorable and one adverse), the increase in P_1 may or may not be a change that improves crashworthiness significantly.

2. Increase in Energy Absorption Capacity of a Given Bay

Without changing the force required to initiate collapse of the first bay, the energy absorbed by the first bay may be increased by improving the shape of the load-deflection curve, For example, if, through additional structure or the use of foamed plastic reinforcement, the average collapse load were increased by 50 percent, the critical velocity for the nose-only impact (of the Piper J-3) would increase from 34 miles per hour to 45 miles per hour. Since the maximum force on the first bay remains unchanged, the deceleration experienced by the cabin floor would remain the same.

3. General Geometric Changes

If the first bay were lengthened by 50 percent while the maximum collapse force was maintained, the critical velocity for the nose-only impact (Piper J-3) would increase from 34 miles per hour to 39 miles per hour. No change in cabin deceleration would accompany this.

4. Change in Component Design

If a change were made in the main landing gear so as to increase its energy-absorbing capacity (for collapse) by a factor of two, then the critical vertical velocity (Piper J-3) would be increased from 10 miles per hour to 12.2 miles per hour.

5. Seat and Seat Tie-Down Changes

The present investigation is not concerned with the relationship between occupant tie-down effectiveness and survivability; however, in the vertical type of impact (main gear only), the stiffness of the seat and cushion combination enters into the analysis. The relative improvement in crashworthiness effected by minor seat changes as against major structure changes is of interest. If, for example, the seat and cushion stiffness were reduced by a factor of two, the critical vertical velocity would increase from 10 miles per hour to 12.2 miles per hour.

6. Minor Structural Changes

It does not appear that a minor structural change such as strengthening a single tube or adding a brace will significantly improve crashworthiness. The possible exception lies in the cabin structure, which is treated later.

The tubular structure airframe is generally redundant to the point that the strength of any one tube will not greatly affect the collapse load of a bay or section of the airframe.

7. Changes Designed to Reduce Effective Mass of Aircraft upon Impact

If the effective mass of an aircraft were reduced upon impact by design changes (such as design features to cause wings and tail section to separate from the cabin), a reduction in critical velocity could be effected (for nose-only-impact configuration). For example, if it were possible to reduce the effective mass by one-third (in the Piper J-3 during nose impact, the critical velocity would increase from 34 miles per hour to 42 miles per hour. At the same time, the cabin deceleration would increase, from 30.2 G to 45 G. Again, such a change produces both beneficial and adverse effects, and the net benefit attained would depend upon the relative significance of these effects.

8. Cabin Modification

Although not investigated in detail during this analysis, improved crashworthiness appears to be probable from changes in cabin structure. If the cabin (defined as the protective shell immediately surrounding the occupants) were made much stronger than other airframe structure, impact forces could be transmitted through the cabin and the energy absorbed in deformation elsewhere. The present analysis has considered cabin strength only to verify that, for the particular forces associated with a given collapse mode, the cabin would remain essentially intact.

LIMITATIONS OF ANALYSIS

The analysis made so far contains both limitations and uncertainties. As it was developed for the tube and fabric type of light aircraft and was in particular oriented toward the Piper J-3 type of construction, it would be inadequate for any aircraft that departs greatly from the Piper J-3 in structure. Moreover, the analysis contains several arbitrary assumptions that deserve further investigation. Static loading is assumed on any given substructure. An intermediate value of column-end fixity is assumed. Simplifying assumptions were made in connection with energy dissipation. In the computations (reference the Appendix), values were assumed both for the soil elastic constant and for the effective mass of the aircraft. These assumptions presently stand as uncertainties.

RESULTS OF NUMERICAL EXAMPLES

The following table summarizes the results of computations (reference the Appendix) for the three impact configurations discussed using the Piper J-3 as an example aircraft design. The critical velocity is understood to mean that velocity (for the mathematically idealized conditions) that produces airframe collapse just short of cabin collapse, or, in the case of vertical velocity, that produces a vertical deceleration of 20G.

Impact Configuration	Critical Velocity (mph)	Maximum Deceleration (G)
Nose-only contact	34.0	30.2
Nose-and-wing contact	35.7	33.6
Gear-only contact (vertical velocity)	10.0	20.0

APPENDIX. NUMERICAL EXAMPLES

The computations for the Piper J-3, associated with equations 3, 4, 7, 8, 9, 10, 13, 15, 16, 17, 18, and 19, appear on the attached data sheets.

Tube data were taken from Piper structural drawings. The effective mass for the entire aircraft was arbitrarily taken as 90 percent of the maximum gross weight. This is based upon presumed flailing of occupants, shifting of luggage, and sloshing of fuel. The soil spring constant (3000-pounds-per-inch penetration) is based upon reported penetrations and force-mass-acceleration data of the NACA crash tests using Piper J-3 aircraft which were crashed into tamped earth embankments. The gear collapse energy for the Piper J-3 may be computed from elastic data on stretch of bungee cords (four loops). The stretch as installed on the aircraft is 4 inches, and collapse stretch is an additional 25.2 inches.

FIRST BAY FORCE COMPUTATIONS

Piper J-3

Member	x_i / y_i / z_i	$\bar{x}_i$ / $\bar{y}_i$ / $\bar{z}_i$	$(\bar{x} - x)$ / $(\bar{y} - y)$ / $(\bar{z} - z)$	L^2 / L	λx / λz	d_i / t_i / Le*	F_{cr} / F_y	$F_{cr_i}\lambda x_i$
1	0 9.75 -1.5	17.0 13.5 -25.0	17.0 3.75 -23.5	305 17.5	.971 .085	.59 .035 16.5	6.12 4.54	4.41
2	0 9.75 -1.5	26.2 12.25 -25.0	26.2 2.5 -23.5	1245 35.3	.743 -.666	.715 .035 33.0	2.73 5.50	2.03
3	0 9.25 -20.0	26.2 0 -25.0	26.2 -9.25 -5.0	797 28.2	.927 -.177	.59 .035 26.0	2.47 4.54	2.29
4	0 9.75 -20.0	26.2 12.25 -25.0	26.2 2.5 -5.0	718 26.8	.977 -.186	.59 .035 25.0	2.67 4.54	2.61
5	0 0 -20.0	17.0 13.5 0	17.0 13.5 20.0	871 29.5	.576 .677	.653 .035 26.0	3.34 5.02	1.92

*For the purpose of computing buckling loads, the effective length of the tube, Le, was used, the length of uniform cross-section of the tube.

$$\frac{1}{2} \sum F_{cr_i}\lambda x_i = 13.26$$

$$P_i = 26.5 \text{ kips}$$

Maximum deceleration for nose-only contact (assuming effective mass of .9 of actual mass):

$$P_1 = m'a \qquad a = \frac{P_1}{m'} \qquad m' = \frac{(.9)(.97)}{G} \text{ kips}$$

$$a = \frac{26.5}{.873} G$$

$$a = 30.4 \text{ G}$$

Use:

K = 3 kips/inch (based upon data from NACA crash tests)

s = 17 inches

m' = (.9)(970) = 873 lb.

m = (.9)(1200) = 1080 lb.

Therefore,

$$Z^2 - \left(2 + \frac{P_1}{Ks}\ \frac{m}{m'}\right) Z + \left(1 + \frac{P_1}{Ks}\right) = 0$$

$$Z^2 - \left[\ 2 + .52\,(1.235)\right] Z + (1 + .52) = 0$$

$$Z = 1.32 + \sqrt{1.74 - 1.52} = 1.79$$

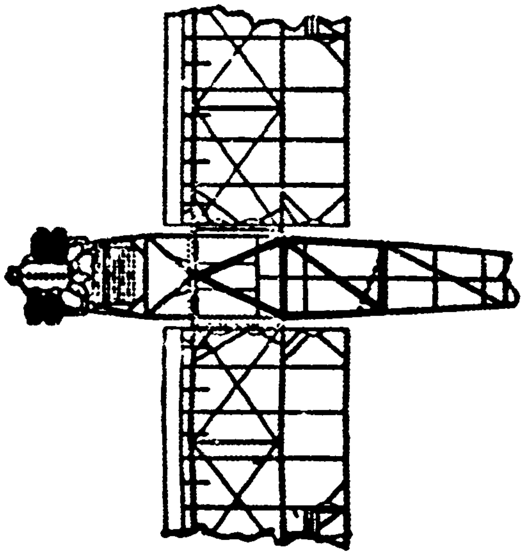
$$v^2 = \frac{P_1 Zs}{m'} = \frac{(26.5)(1.79)(17)(32.2)}{(.873)(12)}$$

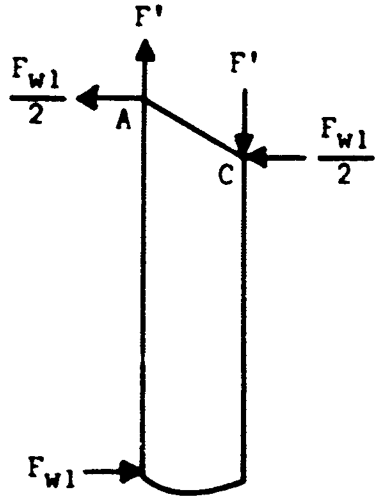
$$= 2478$$

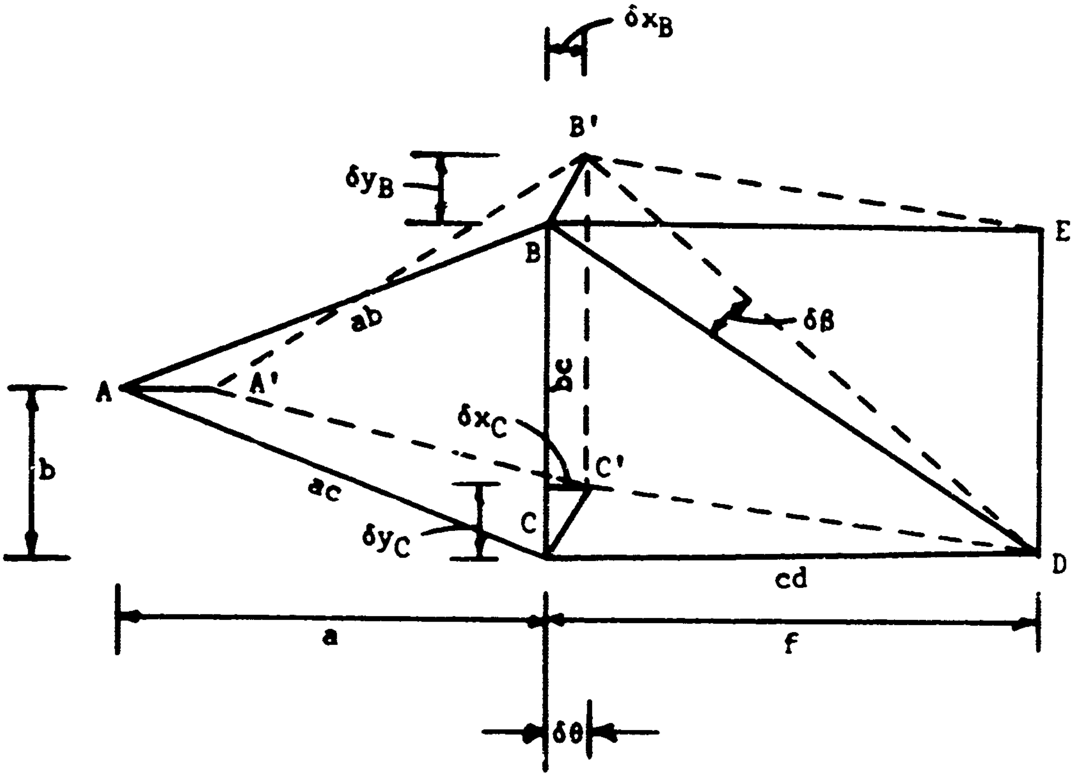
$$v = 49.8 \text{ fps} \sim 34 \text{ mph}$$

WING ROTATION MECHANISM

Plan Views of Wing Attachment







b = 12"
a = 31"
f = 36.5"
r = 175"

Small Angle Geometry

$$\delta y_B = \delta y_C = a\delta\theta$$

$$= f\delta\beta$$

$$\text{Therefore, } \delta\beta = \frac{a}{f}\,\delta\theta$$

$$\delta x_B = 2b\delta\beta$$

$$= 2b\,\frac{a\delta\theta}{f}$$

$$\delta x_C = 2b(\delta\beta + \delta\theta)$$

$$= 2b(1 + \frac{a}{f})\,\delta\theta$$

An upper bound to the external load, F_{w1}, required to sustain the assumed mechanism may be found by employing the virtual work principle.

$$\delta(\text{External work}) = \delta(\text{Internal work})$$

$$F_{w1} r\delta\theta = \sum F_{cr_i}\delta_i$$

$$F_{w1} r\delta\theta = F_{cd_x}(\delta x_B + \delta x_C)$$

$$= F_{cd_x}\left[\frac{2b}{f}\,a\delta\theta + 2b\left(1 + \frac{a}{f}\right)\delta\theta\right]$$

$$F_{w1} = F_{cd_x}\,\frac{2b}{r}\left[\frac{2a}{f} + 1\right]$$

$$= (2.79)\,\frac{(2)(12)}{175}\,(2.7) = 1.04 \text{ kips}$$

The upper bound would also be a lower bound if equilibrium of internal forces were satisfied at each joint. A consideration of each joint free body shows that equilibrium can be satisfied by appropriate internal forces compatible with the load, F_{w1}, found above. Thus, F_{w1} is correct.

$$Y^2 - \left[2 + \frac{m}{m'} \; \frac{P_1 + F_{w1}}{Ks} - \frac{F_{w1}}{Ks} \right] Y + \left[1 + \frac{P_1}{Ks} \right] = 0$$

$$Y^2 - \left[2 + .666 - .020 \right] Y + 1.52 = 0$$

$$Y = 1.322 + \sqrt{1.75 - 1.52} = 1.80$$

Therefore,

$$v^2 = \frac{\left(P_1 + F_{w1}\right)sY}{m'\left(1 - \frac{m_w}{3m'}\right)} \qquad m_w = \frac{1.64}{g} \text{ slugs}$$

$$v^2 = \frac{(2.74)(17)(1.80)(32.2)}{(12)(.873)(.9375)} = 2749$$

$$v = 52.4 \text{ fps or } 35.7 \text{ mph} .$$

Deceleration for Nose and Wing Contact

Equation (13) $$F_{w2} = \frac{P_1 + F_{w1}}{3\,\frac{m'}{m_w} - 1} = \frac{26.5 + 1.04}{3(5.32) - 1} = 1.84 \text{ kips}$$

Maximum possible force = $P_1 + F_{w1} + F_{w2}$

$$F = 26.5 + 1.04 + 1.84 = 2.94 \text{ kips}$$

$$a = \frac{F}{m'} = \frac{29.4}{.873} ? = 33.7G$$

Where

w_m is the occupant weight = 170 lb.

k_s is the assumed seat elastic constant = 1700 psi

For maximum deceleration of 20g,

$$F = ma$$

$$k_s\Delta = \left(\frac{170}{g}\right)(20g)$$

$$\text{or } \Delta = 2 \text{ inches.}$$

Then, using Equation (18),

$$v_1 = \sqrt{\frac{k_s}{m_m}}$$

$$v_1 = \sqrt{\frac{(1700)(12)(32.2)}{170}}\ \frac{2}{12}$$

$$v_1 = 10.35 \text{ fps .}$$

For the Piper J-3, the energy of main gear collapse may be obtained from the load deflection curve of the bungee cord used:

$$U_g = \underbrace{\left[P_o x + \frac{kx^2}{2}\right]}_{\text{energy per cord}} \text{ times number of bungee cords stretched}$$

where

P_o is initial tension in cord = 100 lb.

x is total elongation of each cord = 25.2 in.

k is cord spring constant (from load deflection curve) = 9.16 lb-in. elongation.

(Each cord loop is 28 inches long unstretched).

Therefore, for the Piper J-3,

$$U_g = \left[\frac{(100)(25.2)}{12} + \frac{(9.16)(25.2)^2}{24}\right] 4$$

$$U_g = 1812 \text{ ft-lb.}$$

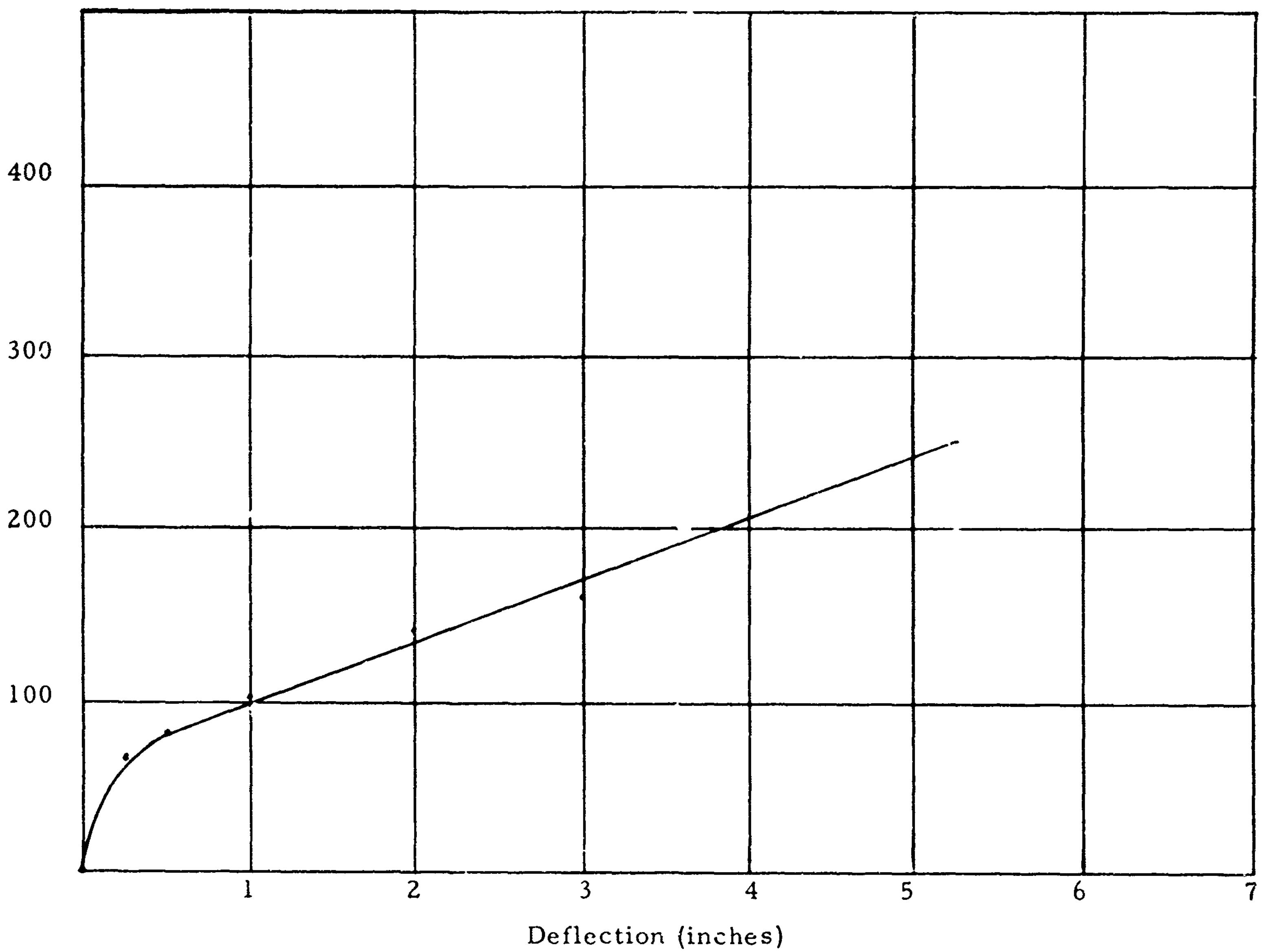
Then, using Equation (19),

$$v_o = \sqrt{\frac{2U_g}{m} + v_1^{\,2}}$$

$$v_o = \sqrt{\frac{(2)(1812)(32.2)}{1051}} - (10.35)^2$$

$$v_o = 14.65 \text{ fps}$$

$$\sim 10 \text{ mph} .$$



Load-Deflection Curve for Piper J-3 Landing Gear Bungee Cord

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